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ABSTRACT

The mechanical properties of different adhesives at elevated temperatures can change differently due to the differences in adhesive molecular chain structure. Therefore, a profound understanding of the effect of these property changes on the bond behaviour of carbon fibre reinforced polymer (CFRP)-to-steel bonded joints is of great importance when designing bonded CFRP strengthening systems for steel structures. Existing studies on CFRP-to-steel bonded joints under monotonic loading have clearly shown that both adhesive mechanical properties and geometrical properties of the bonded joints (e.g. bond length) may significantly influence the bond strength. Existing studies on adhesive mechanical properties under elevated temperatures have shown that the variation of adhesive mechanical properties, especially fracture energy with temperature depends significantly on the adhesive type. No comprehensive study exists so far on understanding the effects of key mechanical and geometrical parameters of a CFRP-to-steel bonded joints at elevated temperatures on bond strength. This paper presents a study aimed at understanding the effects of different parameters such as temperature dependent mechanical properties of adhesive and bond length on the behaviour of CFRP-to-steel bonded joints at elevated temperatures. Results of this study showed that (1) load-displacement behaviour of the bonded joints is sensitive to temperature variations, (2) for bonded joints with sufficiently long bond length, the ultimate load depends

only on the fracture energy of the final temperature, and (3) the maximum load of the bonded joints depends on the ratio between the loading and heating rates.

Keywords

CFRP-to-steel; FDM; bond-slip relation; temperature change; bond length.

1. Introduction

Externally bonded (EB) fibre reinforced polymer (FRP) laminates have become increasingly popular within the structural engineering community as an effective way of flexural and shear strengthening of structures [1-4]. The load carrying capacity of such structures strengthened with EB FRP laminates often relies on the interfacial shear stress transfer mechanism of the bonded interface [5, 6]. Therefore, numerous studies have been carried out to investigate the bond behaviour of FRP-to-concrete [7-9] and FRP-to-steel [6, 9-11] bonded joints under mechanical loading. Different models have been proposed to model the constitutive behaviour of the FRP-to-steel and FRP-to-concrete bonded interfaces [6, 12] as well as the behaviour of structures strengthened EB FRP laminates [13-16]. However, the behaviour of FRP-to-concrete or FRP-to-steel structures under varying temperatures has received much less attention. FRP strengthened structures are likely to experience significant temperature variations during their life-cycle, especially within the range of glass transition temperatures (T_g) of typically used adhesives. As the mechanical properties of these adhesives vary significantly at temperatures close to their T_g, temperature variations will clearly affect the bond performance of structures strengthened with EB FRP laminates. Therefore, a better understanding of the bond behaviour under varying temperature conditions is necessary.

The load carrying capacity of steel structures with flexural strengthening with EB FRP laminates relies on the shear stress transfer mechanism of the adhesive layer and the dominant failure mode of a properly prepared [6] bonded joints is cohesion failure within the adhesive [10]. Therefore, changes of adhesive properties with temperature may significantly affect the performance of the bonded joints, thus the strengthened structure. Different to FRP-to-steel bonded joints, the dominant failure mode of FRP-to-concrete bonded joints under monotonic mode II loading is typically cohesive failure of concrete a few millimetres from the adhesive-concrete bi-material interface [7]. Nevertheless, with increases in temperature, the failure mode of such bonded joints may change from cohesive failure within the concrete to cohesive failure within the adhesive due to changes in adhesive mechanical properties with temperature [17, 18]. Therefore, for both FRP-to-concrete and FRP-to-steel bonded joints, variations of adhesive properties due to temperature changes may significantly affect the behaviour of the bonded joints.

Some research has been done to study the temperature effect on the mechanical properties of different adhesives [19-26]. The effect of temperature on adhesive mechanical properties such as strength, elastic modulus, and fracture energy were studied. For single-lap joints made using American Cyanamid FM 1000 epoxide-polyamide film adhesive, a significant drop in the strength of the adhesive was observed with increasing temperature [19]. However, the rate of bond strength reduction with temperature in the single-lap joints strength was less than the rate of adhesive strength decrease with temperature. Similar observation was also reported in [20] where Araldite 420 was used to bond the carbon FRP (CFRP) to steel. On the contrary, it was found that the bond strength of the CFRP-to-steel bonded joints using Sikadur 330 showed an increasing trend with the increasing temperature when the temperature was below the T_g and then decreased significantly with further increase in temperature [21]. However, due to short bond length (less than 100mm) used in these tests, if the effective bond length was longer than 100mm, then bond length may also have an effect on the bond strength. Pull-off tests of CFRP-to-steel bonded joints with 150mm and 200mm bond length at different temperatures were presented in [22] and [23] respectively. It was reported that the bond strength decreased with temperature significantly [22]. However, the exact adhesive used to bond CFRP-to-steel was not specified. When Spabond 345 was used to bond the FRP and steel plate, an increase in the load carrying capacity of FRP-to-steel bonded joints was observed when the temperature was below its T_g [23]. For SikaPower-978 adhesive, both mode I and mode II fracture energy, elastic stiffness and strength was found to decrease with increasing temperature within the range between -30 °C to 80 °C [26]. For some cementitious adhesive systems, the fracture energy was shown to initially increase with temperature till 400 °C to 600 °C and then followed by a decreasing trend [24]. Similar research by others [25] have shown that temperature has a considerable effect on the adhesive properties in terms of the ductility and the strength. Results from the above research clearly indicate that the adhesive mechanical properties are significantly affected by the temperature, while the effects are dependent on the type of adhesive.

Several experimental studies have been conducted on FRP-to-concrete bonded joints exposed to different temperature variations [17, 18, 27-29]. In the existing experiments on FRP-to-concrete bonded joints under different temperatures, single-shear pull-off test has been often used to determine the bond-slip relation, which is essential in modelling the constitutive behaviour of the bonded interfaces. Some studies have shown that initial thermal stresses may have a significant effect on the maximum load [30], thus when obtaining interfacial fracture energy from the ultimate load, consideration of the initial thermal stresses is important. If a numerical approach is used to determine the initial thermal stresses within the bonded interface, it is important to use an appropriate temperature dependent constitutive model to model the behaviour of the bonded interface. Dai, et al [31] proposed a pseudo temperature dependent bond-slip model for FRP-to-concrete bonded joints based on the experimental studies. In their study, the effects of the thermal stresses were considered in deriving the interfacial fracture energy. The proposed model assumed that the interfacial fracture energy remained constant at $T < T_g$ of the adhesive and decreased at temperatures higher than T_g. However, in CFRP-to-steel bonded joints under mode II loading, while both elastic modulus and interfacial shear strength reduced with temperature (below T_g), load carrying capacity and fracture energy was found to increase initially. Beyond T_g, load carrying capacity reduced significantly. However, comparing the axial strain distribution of the CFRP plate at higher temperatures (above T_g), it was found that the whole bond length was activated in resisting the load before the peak load was reached, thus insufficient bond length may have affected the load capacity [32].

The discussion above shows that different adhesives give place to varying changes in bond behaviour when the temperature is increased. Ultimately, these variations are the consequence of the intrinsic mechanical behaviour of adhesives. The major reason for this is that virtually all technically demanding adhesive systems are based upon synthetic polymers [33], whose mechanical properties are controlled by the strongly temperature-dependent van der Waals' forces between the molecular chains and the covalent bonds along the polymer chain backbone [34]. While, existing studies clearly demonstrate that the bond behaviour is affected by the temperature variations, no systematic study has been conducted on FRP-to-concrete or FRP-to-steel bonded joints addressing how different parameters (e.g. interfacial shear strength, bond-slip behaviour, interfacial fracture energy, effective bond length, and rate of loading) may affect the behaviour of such bonded joints under varying temperature conditions.

Against this background, this study presents a theoretical investigation into the effect of different parameters such as, interfacial shear strength, bond-slip behaviour, interfacial fracture energy, bond length, and rate of loading on CFRP-to-steel bonded joints under varying temperatures. To investigate the effect of adhesive mechanical property change under temperature change on the bond behaviour of such bonded joints under the combination of sustained load and temperature change, a finite difference method (FDM) [35] which allows force controlled loading was adopted. Then the effect of different mechanical loading and thermal loading combinations, bond length, and different fracture change trends at elevated temperatures was further investigated. This study, while theoretical, presents valuable insight into better understanding of the effects of different parameters on the behaviour of FRP-to-steel bonded joints under varying temperatures.

2. Analytical solution

2.1. Bond-slip behaviour

An idealized CFRP-to-steel bonded joints is shown in Fig. 1. Both adherents are assumed to be subjected only to axial forces. The thickness of all constituents is assumed to be constant along the length. Adhesive layer was assumed to be subjected to uniform interfacial shear stresses across the thickness as commonly assumed in well accepted interfacial stress solutions of similar bonded joints [6, 13, 36].

Relationship between the interfacial shear stress and slip was assumed to be given by a bilinear bond-slip model (Fig. 2) as adopted in many other studies [12-14]. No damage was assumed within the linear ascending branch of the bond-slip curve, while damage elasticity with linear unloading as shown in Fig. 2 was assumed within the softening range (i.e., descending branch of the bond-slip curve) [13]. For an interface with a load applied prior to the temperature change, it is assumed that the temperature elevation will not change the damage state of the node (Fig. 2).

2.2. FDM for CFRP-to-steel bonded joints with temperature change

FDM approach adopted in this paper is similar to the approach described in Carrara and Ferretti [35]. For the completeness of the current paper, FDM solution for simulating the behaviour of CFRP-to-steel bonded joints under mode II loading is briefly presented in this section. For further details of the FDM, readers are referred to Carrara and Ferretti [35].

Considering the equilibrium of the segment shown in Fig. 1, the governing equation for a CFRP-to-steel bonded joints can be expressed as follows:

$$\frac{d\sigma_p}{dx}-\frac{\tau}{t_p}=0 \tag{1}$$

Where σ_p and t_p is the axial stress and thickness of the CFRP plate respectively. τ is the interfacial shear stress induced within the element of consideration.

The slip between the CFRP plate and the steel substrate can be written as the difference between the displacement of CFRP plate u_p and the steel substrate u_s [37].

$$\delta=u_p-u_s \tag{2}$$

The first derivative of Eq. (2) with respect to x can be written as:

$$\frac{d\delta}{dx}=\frac{du_p}{dx}-\frac{du_s}{dx} \tag{3}$$

Assuming linear elastic behaviour for both CFRP and steel, following relationship can be derived for the axial stresses in CFRP plate (σ_p) and steel substrate (σ_s):

$$\sigma_p = E_p\left(\frac{du_p}{dx} - \alpha_p \Delta T\right) \tag{4}$$

$$\sigma_s = E_s\left(\frac{du_s}{dx} - \alpha_s \Delta T\right) \tag{5}$$

In which E_p and E_s represent the elastic modulus of CFRP plate and steel substrate respectively, and α_p and α_s are the thermal expansion coefficient of the CFRP plate and steel substrate respectively. Any thermal expansion in the adhesive layer was neglected due to the significantly lower stiffness of the adhesive layer compared to that of the adherends.

Meanwhile, the axial stress of CFRP plate and the steel substrate can be calculated using the axial force N as:

$$\sigma_p = \frac{N}{A_p} \tag{6}$$

$$\sigma_s = -\frac{N}{A_s} \tag{7}$$

Where A_p and A_s are the cross sectional area of the CFRP plate and steel substrate. Combination of Eqs. (3)-(7) yields:

$$\frac{d\delta}{dx} = \frac{N}{E_p A_p} + \alpha_p \Delta T + \frac{N}{E_s A_s} - \alpha_s \Delta T \tag{8}$$

Eq. (8) can be rewritten as:

$$\frac{d\delta}{dx} = \left(\frac{1}{E_p A_p} + \frac{1}{E_s A_s}\right) N + \left(\alpha_p - \alpha_s\right) \Delta T \tag{9}$$

In order to get the approximation of the first derivative of the slip and axial force, the interface is discretised into n segments, as shown in Fig. 3b. Then, an approximation of the differential Eqs. (1) and (9) can be obtained as follows:

$$\frac{N^{i+1} - N^i}{h_n} = \frac{1}{2}\left[\tau^{i+1}\left(\delta^{i+1}\right) + \tau^i\left(\delta^i\right)\right] b_p \tag{10}$$

$$\frac{\delta^{i+1}-\delta^{i}}{h_n}=\frac{1}{2}\left(\frac{1}{E_pA_p}+\frac{1}{E_sA_s}\right)\left(N^{i+1}+N^{i}\right)+\left(\alpha_p-\alpha_s\right)\Delta T \tag{11}$$

Where h_n is the distance between two adjacent nodes, and $\tau(\delta)$ is the constitutive relationship between the interfacial shear stress and interfacial slip which can be given as:

$$\tau(\delta)=(1-D_\delta)K_e\delta \tag{12}$$

In which K_e is the initial slope of the bond-slip relation and D_δ is the damage parameter assuming elastic damage, defined as follows:

$$D_\delta=\begin{cases}0 & when\ \delta\le\delta_1\\ \dfrac{\delta_2(\delta-\delta_1)}{\delta(\delta_2-\delta_1)} & when\ \delta_1<\delta<\delta_2\\ 1 & when\ \delta\ge\delta_2\end{cases} \tag{13}$$

Where δ_1 and δ_2 are the slip value corresponding to the maximum shear stress and full failure respectively.

Substituting the Eq. (12) into Eq. (10) yields:

$$-\frac{N^i}{h_n}-\frac{1}{2}K^i\delta^i b_p+\frac{N^{i+1}}{h_n}-\frac{1}{2}K^{i+1}\delta^{i+1}b_p=0 \tag{14}$$

In which $K^i=\left(1-D_{\delta^i}\right)K_e$.

Similarly, Eq. (11) can be rewritten as:

$$-\frac{1}{2}\left(\frac{1}{E_pA_p}+\frac{1}{E_sA_s}\right)N^i-\frac{\delta^i}{h_n}-\frac{1}{2}\left(\frac{1}{E_pA_p}+\frac{1}{E_sA_s}\right)N^{i+1}+\frac{\delta^{i+1}}{h_n}=\left(\alpha_p-\alpha_s\right)\Delta T \tag{15}$$

Writing Eqs. (14) and (15) into matrix form will return:

$$\begin{bmatrix}-\dfrac{1}{h_n} & -\dfrac{1}{2}K^ib_p & \dfrac{1}{h_n} & -\dfrac{1}{2}K^{i+1}b_p\\ -\dfrac{1}{2}\left(\dfrac{1}{E_pA_p}+\dfrac{1}{E_sA_s}\right) & -\dfrac{1}{h_n} & -\dfrac{1}{2}\left(\dfrac{1}{E_pA_p}+\dfrac{1}{E_sA_s}\right) & \dfrac{1}{h_n}\end{bmatrix}\begin{bmatrix}N^i\\ \delta^i\\ N^{i+1}\\ \delta^{i+1}\end{bmatrix}=\begin{bmatrix}0\\ \left(\alpha_p-\alpha_s\right)\Delta T\end{bmatrix} \tag{16}$$

Different boundary conditions can be considered with regarding the test control method, i.e.,

1) Loaded-end force driven process:

$$B_0=\begin{bmatrix}1 & 0\\ 0 & 0\end{bmatrix},\quad B_n=\begin{bmatrix}0 & 0\\ 1 & 0\end{bmatrix},\quad Z=\begin{bmatrix}0\\ N^l\end{bmatrix} \tag{17}$$

2) Loaded-end displacement driven process:

$$B_0=\begin{bmatrix}1 & 0\\ 0 & 0\end{bmatrix},\quad B_n=\begin{bmatrix}0 & 0\\ 0 & 1\end{bmatrix},\quad Z=\begin{bmatrix}0\\ \delta^l\end{bmatrix} \tag{18}$$

3) Far end displacement driven process:

$$B_0=\begin{bmatrix}1 & 0\\ 0 & 1\end{bmatrix},\quad B_n=\begin{bmatrix}0 & 0\\ 0 & 0\end{bmatrix},\quad Z=\begin{bmatrix}0\\ \delta^0\end{bmatrix} \tag{19}$$

The differential equations can be expressed in compressed form:

$$\begin{bmatrix}C_1 & Q_1 & & & & & \\ & C_2 & Q_2 & & & & \\ & & \ddots & \ddots & & & \\ & & & C_j & Q_j & & \\ & & & & \ddots & \ddots & \\ & & & & & C_{n-1} & Q_{n-1}\\ B_0 & & & & & & B_n\end{bmatrix}\begin{bmatrix}U_1\\ U_2\\ \vdots\\ U_j\\ \vdots\\ U_{n-1}\\ U_n\end{bmatrix}=\begin{bmatrix}R_1\\ R_2\\ \vdots\\ R_j\\ \vdots\\ R_{n-1}\\ Z\end{bmatrix} \tag{20}$$

Among which, U_j is the axial force and slip at the node j and R_j is the external force and thermal expansion at node j. While C_j and Q_j are the matrix block composed of the first and second two rows of elements in Eq. (16), that is:

$$C_j=\begin{bmatrix}-\dfrac{1}{h_n} & -\dfrac{1}{2}K^j b_p\\ -\dfrac{1}{2}\left(\dfrac{1}{E_pA_p}+\dfrac{1}{E_sA_s}\right) & -\dfrac{1}{h_n}\end{bmatrix},\quad Q_j=\begin{bmatrix}\dfrac{1}{h_n} & -\dfrac{1}{2}K^{j+1}b_p\\ -\dfrac{1}{2}\left(\dfrac{1}{E_pA_p}+\dfrac{1}{E_sA_s}\right) & \dfrac{1}{h_n}\end{bmatrix} \tag{21}$$

Eq.(16) can be also rewritten as:

$$\left\{\frac{1}{h_n}\left[\begin{array}{cc:cc}-1 & 0 & 1 & 0\\ 0 & -1 & 0 & 1\end{array}\right]-\frac{1}{2}\left[\begin{array}{cc:cc}0 & K^i b_p & 0 & K^{i+1}b_p\\ \frac{1}{E_pA_p}+\frac{1}{E_sA_s} & 0 & \frac{1}{E_pA_p}+\frac{1}{E_sA_s} & 0\end{array}\right]\right\}\begin{bmatrix}N^i\\ \delta^i\\ N^{i+1}\\ \delta^{i+1}\end{bmatrix} = \begin{bmatrix}0\\ \left(\alpha_p-\alpha_s\right)\Delta T\end{bmatrix} \tag{22}$$

The detailed solving process of Eq. (22) can be found in Martinelli and Caggiano [38]. For a given temperature, the bond-slip relation is updated for the following calculation in every step. The boundary conditions should be selected according to the test control mode.

3. Verification of the FDM

3.1 Numerical modelling

To verify the FDM presented in the above section, the bond behaviour of CFRP-to-steel bonded joints under monotonically increasing load at constant temperature, followed by sustained load and temperature increase, followed by monotonically increasing load until failure at constant temperature was investigated. Both temperature independent bond-slip relation (called constant bond-slip relation hereafter) and temperature dependant bond-sip relation (called varying bond-slip relation hereafter) were considered to investigate the influence of varying bond-slip relations. Results from the FDM were compared with the results from an finite element method (FEM). FEM approach used in this study for modelling the bonded interface was based on well accepted modelling approached for modelling CFRP-to-steel bonded joints [15, 39]. In FEM, both CFRP plate and steel plate are modelled as truss element, while the adhesive was modelled using 2-D cohesive element [39]. Traction-separation law of the cohesive elements were defined to be the same as the interfacial shear stress-slip relationship used in FDM models, which is given by Eqs. (12) and (13). The cohesive elements were tied with FRP plate steel plate. The CFRP plate was constrained in the vertical direction to avoid any peeling effect.

Based on the preliminary test results on CFRP-to-steel bonded joints with Sika 30 adhesive [32], following equations were derived to express the variation of elastic stiffness (K_e), interfacial shear strength (τ_f), and interfacial fracture energy (G_f) with temperature (T in °C):

$$K_e = 1785 \times e^{-0.047T} \left(N/mm^2/mm \right) \quad (23)$$

$$\tau_f = -0.2428 \times T + 21.141 \left(N/mm^2 \right) \quad (24)$$

$$G_f = -0.00206 \times T^2 + 0.1978 \times T - 2.6185 \left(N/mm \right) \quad (25)$$

Given three parameters above (i.e., K_e, τ_f, G_f), the bond-slip relation can then be determined. It can be seen that as temperature increased, τ_f and K_e decreased monotonically. However, G_f increased first and then decreased.

Eqs. (23)-(25) were derived from the preliminary test results [32] of ongoing research at the University of Queensland and, thus, may not represent the true variation of those parameters with temperature. More investigations are necessary before accurate models predicting the variation of bond-slip curves with temperature could be developed. Nevertheless, model given in Eqs. (23)-(25) was deemed adequate to qualitatively demonstrate the effect of varying bond-slip behaviour on the behaviour of CFRP-to-steel bonded joints at varying temperatures. The geometry and material parameters used in this analysis are given in Fig. 3a and Table. 1.

In the simulation, loading and temperature change was applied in three steps; Step 1: temperature was kept a constant at 22 °C while load was increased from 0kN to 20kN, Step 2: load was kept at 20kN and temperature was increased to 42 °C, and Step 3: After the temperature reached 42 °C, temperature was kept a constant and load was increased until failure. In FDM, for Step 1 and 2, the boundary conditions are given in Eq. (17), while Eq. (18) is used for Step 3. If the full range behaviour of the bonded joints is desired, Eq. (19) can be used as the boundary conditions for Step 3.

3.2. Results

3.2.1. Load-displacement behaviour

The load-displacement curves predicted from FDM and FEM with varying bond-slip relations are compared in Fig. 4. While both curves show a good agreement, it can be seen that the maximum load capacity from FDM is slightly higher than that from finite element analysis (FEA). In FDM model, behaviour of the bonded interface is modelled using nonlinear springs connecting nodes between CFRP plate and steel plate. Behaviour of the spring was defined using the bond-slip relationship [40, 41]. In FEM, cohesive elements were used to model the behaviour of the bonded interface. This difference in the approach used to model the interface behaviour in FDM and FEM is believed to be the reason for small differences observed in the load-displacement curves from the two models. Nevertheless, the difference between those two methods is less than 1% in terms of the maximum load capacity, which is considered negligible.

Comparison of the bond-slip relations at two different nodes (as illustrated in Fig. 3) from FEA and FDM with varying bond-slip relation are given in Figs. 5 and 6. As can be seen in both figures, a good agreement between the FEM and FDM was achieved. Based on above results, the FDM model proposed in this paper was considered to be accurate in modelling the behaviour of CFRP-to-steel bonded joints at varying temperatures.

3.2.2. Effect of temperature dependant bond-slip behaviour

In order to determine the effect of varying bond-slip behaviour, behaviour of a CFRP-to-steel bonded joints with constant bond-slip curve (at 22 ^{o}C) is compared with the behaviour of the same bonded joints when bond-slip behaviour was assumed to vary with temperature as given in Eq. (23)-(25). The results are compared in Fig. 4. It can be observed that when a varying bond-slip relation is considered the load-carrying capacity is significantly higher than when a constant bond-slip relation is under consideration. Which is essentially due to the increasing fracture energy with the increasing temperature in a varying bond-slip relation.

Moreover, during the temperature elevation process at constant load, displacement at the loaded-end decreased when constant bond-slip relation was used, but displacement at the loaded end was shown to increase when varying bond-slip relation was used. When a constant bond-slip model was used, the stiffness of the adhesive layer was assumed to be a constant throughout the temperature elevation process under constant load. Therefore, due to the differential thermal expansion of the CFRP and steel (CFRP with a lower thermal expansion coefficient than steel), a negative slip in the bonded interface (i.e., opposite direction to the movement due to axial load) resulted, thus reducing the overall slip value at the loaded end. However, when the stiffness of the bond-slip curve was assumed to reduce with the temperature, reduction in the stiffness tended to increase the interfacial slip for a given load, thus counteracting to the decrease in slip resulting from the difference in thermal expansions of CFRP and steel.

Comparison of the bond-slip relations at two different locations (I and II in Fig. 3b) along the bond length from FEM and FDM with varying bond-slip relation are compared with the results from FEM with a constant bond-slip relation and the bond-slip relation at 42 °C in Figs. 5 and 6. For the stress and slip results from the FDM, the average value of the two adjacent nodes were used. In Fig. 5, when the temperature increase at a constant load was applied (i.e., Step 2), a reduction in the slip values in local bond-slip behaviours was observed in FEM with constant bond-slip relation. When the load increased again, the slip was increased, and followed the envelope curve of the constant bond-slip relation (Fig. 5). Bond-slip behaviour at location II (Fig. 3b) from the FEM and FDM models with varying bond-slip relation, tends to initially follow the bond-slip curve at 22 °C until the temperature increase was applied (Fig. 5). Once the temperature was increased, even though the load was kept a constant, a reduction in the interfacial shear stress with increases in the shear slip could be observed. However, the curve deviates from the envelope curve at 22 °C and the slip tends to increase when the temperature increase was applied, while unloading of the bond-slip curve was observed in the constant bond-slip case. Further away from the loaded end, i.e., location II, interfacial shear stresses found to be minimal before the temperature increase, and bond-slip curve followed the curve at 22 °C. Once the temperature was increased at a constant load, increases in interfacial shear stresses and shear slip but a reduction in the stiffness could be observed in the local bond-slip curves in models with varying bond-slip relation. Once the temperature reached 42 °C, local bond-slip curves from the models with varying bond-slip relation converged towards the bond-slip curve at 42 °C (Fig. 6). Comparison of the local bond-slip behaviour of location I and II (i.e., Figs. 5 and 6) from FEM and FDM with a varying bond-slip relation indicates that, final bond-slip behaviour at a node depends both on the temperature variation as well as the load.

Comparison of the evolution of the damage parameter with slip at locations I and II are given in Figs. 7 and 8 respectively. Damage parameter was obtained from the bond-slip curves at different temperatures at respective locations of FEM and FDM models. Variation of the damage parameter with slip of the models with constant and varying bond-slip relations remained the same until step 2, where temperature was increased while load was kept a constant. In Fig. 7 its clearly seen that while damage parameter was kept a constant during temperature increase, the slip from models with constant bond-slip relation decreased, while slip from models with varying bond-slip relation increased. This results in a completely different damage parameter variation with slip between models with constant and varying bond-slip relations during further increase of load until debonding while keeping temperature a constant.

From the results and discussions presented in this section, the bond-slip behaviour at nodes is clearly affected by both temperature variations and loading conditions. Therefore, using a constant bond-slip relation to predict the behaviour of CFRP-to-steel bonded joints under varying/elevated temperature is not appropriate. In order to accurately capture the bond behaviour, temperature dependant bond-slip relation should be used.

4. Effect of temperature dependent fracture energy, loading and heating rates on the bond behaviour

The previous section verified the FDM presented in this paper for analysing the behaviour of single-shear pull-off tests under mode II loading, and demonstrated the significance of considering temperature dependence of the bond-slip behaviour in analysing CFRP-to-steel bonded joints. This section takes a closer look at the effect of temperature dependent fracture energy, and loading and heating rates on the behaviour of CFRP-to-steel bonded joints. In order to minimize any effect due to bond length, significantly large bond length, i.e., 600mm was assumed in all analysis in this section. To investigate the effects of loading and heating rates on the behaviour of CFRP-to-steel bonded joints, 3 different temperature-to-load rates as shown in Fig. 9 were used in this section. Temperature was assumed to linearly increase until 75 ºC (at 0.212s) and then kept as constant. Three different load cases, i.e., L-Case 1, L-Case 2 and L-Case 3 at displacement rates of 3mm/s, 10mm/s and 25mm/s were used in the analysis.

The property change of adhesive was obtained using the test data presented in Walander [26]. As indicated in Walander [26], the elastic stiffness, peak shear stress, and fracture energy decrease with temperature (as shown in Figs. 10a, b and c). In order to demonstrate the effect of fracture energy increase with temperature, an assumed adhesive with the same elastic modulus, peak shear stress, but opposite fracture energy to that obtained from Walander [26] was used (Figs. 10a,b and d). CFRP-to-steel bonded joints were analysed using the FDM procedure described previously in this paper. Thermal loading was applied to the whole bonded joints while mechanical loading was applied at the loaded end.

Fig. 11 shows the load-displacement curve of CFRP-to-steel bonded joints with fracture energy decreasing and increasing with temperature. For both scenarios, ultimate load of the CFRP-to-steel bonded joints under different loading-to-heating rates tends to converge to the same value once the final temperature is reached (Fig. 11). However, ultimate loads in the fracture energy increasing scenarios were larger than that in fracture energy decreasing scenarios. In addition, load-displacement behaviours of the bonded joints were shown to be dependent on the loading-to-heating rate, with L-Case2 and L-Case3 curves in fracture energy decreasing scenario showing an initial load increase followed by a load decrease with increase in displacement (Fig. 11a). Load-displacement curve of L-Case1 in fracture energy increasing scenario (Fig. 11b) showed different behaviour, with load always increasing or remaining constant with increase in displacement. In order to explain these differences in load-displacement behaviour, it is important to look into the local bond-slip behaviours at different points along the bond length of CFRP-to-steel bonded joints.

A comparison of the bond-slip relations at loaded end (i.e., $X_l = 0mm$) and 100mm away from the loaded end (i.e., $X_l = 100mm$) in each loading case is depicted in Fig. 12 for fracture energy decreasing scenario and in Fig. 13 for fracture energy increasing scenario. In both scenarios and all load cases, bond-slip behaviours were found to be varying between the bond-slip curves at 22 °C and 75 °C. In both fracture energy decreasing and increasing scenarios, peak shear stress of the bond-slip curve at $X_l = 0mm$ was found to be higher than that at $X_l = 100mm$. This difference in peak interfacial shear stress tends to reduce with the increase in load rate-to-temperature rate ratio. Axial strain distribution along the bond length depends on the applied load and the local bond-slip behaviour. For a given load, the slip at the loaded end will be higher than the slip at a point away from the loaded end. Therefore, the loaded end will always reach the peak interfacial shear stress earlier than the bonded interface at $X_l = 100mm$. The temperature the bonded interface at the loaded end will reach a certain interfacial shear stress will always be lower than the temperature the bonded interface at $X_l = 100mm$ reaches the same interfacial shear stress. Therefore, considering the temperature difference at loaded end and $X_l = 100mm$ when the same stress value is reached, and the increase in temperature tends to reduce interfacial shear strength, the peak interfacial shear strength of $X_l = 100mm$ can be expected to be lower than that at the loaded end. Increases in loading-to-heating rate reduce the difference in temperature when the peak interfacial shear stress is reached at points $X_l = 0mm$ and $X_l = 100mm$, thus reducing the difference in peak interfacial shear stress. Slip at debonding was shown to be the same at $X_l = 0mm$ and $X_l = 100mm$ when the loading-to heating rate is low (Figs. 12a and 13a), but was shown to reduce for higher loading-to-heating rates (Figs. 12b and c and 13b and c). For higher loading-to-heating rates, reduction in slip at debonding was more pronounced for increasing fracture energy scenario. In the bond-slip models used in fracture energy increasing scenario, significant increase in the slip at debonding was used, which essentially results in pronounced difference in slip at debonding observed in Figs. 13b and c.

In both scenarios, the overall difference of bond-slip behaviour at two different points $X_l = 0mm$ and $X_l = 100mm$ becomes less significant in L-Case 3. The slip at debonding in the fracture energy increase scenario is larger than that in the fracture energy decreasing case. Comparison of the bond-slip relation at the loaded end is presented in Fig. 14. Nevertheless, it can be seen that the bond-slip relation varies when the mechanical loading rate is different in both fracture energy increasing and decreasing scenarios. Specifically, the ultimate slip decreases when the mechanical loading rate increases. Since when the mechanical loading rate is higher, debonding will occur at a lower temperature thus resulting in a lower debonding slip than when mechanical loading rate is lower, where the bonded interface is able to reach a higher temperature for a given load, thus a higher debonding-slip. This difference in slip for different loading rates was much less pronounced for fracture energy decreasing scenarios, which is expected as the difference in ultimate slip of the bond-slip relation with increasing temperature is also less significant when fracture energy decreases with temperature.

Ultimate load of a CFRP-to-steel bonded joints with an adequate bond length subjected to mode II loading is proportional to the square root of the interfacial fracture energy [6, 42]. Trends shown in Figs. 12 and 13 clearly indicate that the resulting bond-slip behaviour varies along the bond length. This also means that that fracture energy varies along the bond length. Trends shown in Fig. 14 show that the bond-slip behaviour, thus the fracture energy also varies with the loading-to-heating rate. From the above observations, it is clear that the fracture energy at the loaded end may not necessarily govern the ultimate load of the bonded joints. In different combinations of fracture energy variation and loading-to-heating rates, the slip at the ultimate load tends to vary (Fig. 11). It is evident when the bond-slip behaviour at the loaded end for L-Case 2 (Fig. 14a) and load-displacement curve of fracture energy decreasing scenario for L-Case2 (Fig. 11a) are compared, ultimate load is reached when the loaded end displacement is 0.62mm, which is smaller than the slip at debonding at the loaded end (0.65mm) (Fig. 14a). On the other hand, for fracture energy increasing scenario with L-Case2, ultimate load is reached when loaded end slip is 2.12mm, which is greater than the debonding slip at the loaded end (0.81mm) (Fig. 13b). This could be further illustrated by looking at the interfacial shear stress distributions of CFRP-to-steel bonded joints under L-Case2 at different loading points of the load-displacement curve (Fig. 15).

At both points A and B, interfacial shear stresses at the loaded end are non-zero thus indicating no debonding at the loaded end. However, as the displacement further increases, at point C, interfacial shear stresses closer to the loaded end can found to be zero indicating debonding within that region. It can also be noted that from point A to B even though the load increased, peak interfacial shear stress has reduced. However, as debonding at the loaded end had not been reached, further transfer in the stresses contributed towards increasing the total load. A region close to the far end also shows shear stresses, which is due to different thermal expansion of CFRP and steel plate of consideration.

In summary above discussion showed that: (a) Resulting fracture energy varies along the bond length and is dependent on the rate of fracture energy variation with temperature as well as the loading-to-heating rate; (b) Ultimate load of the bonded joints may not necessarily be dependent on the resulting fracture energy at the loaded end, and is significantly dependent on the variation of the bond-slip behaviour along the bond length; and (c) Provided adequate bond length exists, load capacity at the final temperature is independent of the loading-to-heating rate and is dependent on the interfacial fracture energy of the final temperature.

5. Effect of bond length

Analysis presented in the previous section minimized the effect of bond length by selecting a large enough bond length. In that study, it was revealed that the bond-slip behaviour varies along the bond length, thus indicating that for bonded joints with shorter bond lengths, bond length may affect the bond behaviour. In order to investigate this, behaviour of a CFRP-to-steel bonded joints with a bond length of 50mm was analysed using the previously outlined FDM procedure. The bond length selected, 50mm, was lower than the effective bond length at 22 ^{o}C (i.e., 83.5mm). Another three loading cases, i.e., and 0.3mm/s, 0.6mm/s and 0.9mm/s were used in the analysis. These new loading rates, which were lower than the loading rates used in the previous section, were adopted to ensure adequate temperature difference is reached before full damage occurs within the selected shorter bond length. Except for bond length and mechanical loading rate, all other parameters were kept equal to 600mm long specimens discussed in the previous section.

Fig. 16 depicts the load-displacement curve of 50mm long bonded joints in two scenarios, while the maximum load carried by the bonded joints is given in Table 2. It is obvious that the maximum load-carrying capacity is less than that of bonded-joints with 600mm bond length, which is expected due to the shorter bond length (which is lower than the effective bond length at 22 ºC) used. In both fracture energy decreasing and increasing scenarios, ultimate load tends to increase with the increase in loading-to-heating ratio (Fig. 16). Ultimate load for each load case also tends to be higher for the fracture energy increasing scenario than the fracture energy decreasing scenario.

When the bond length is less than the elastic effective bond length (i.e., minimum bond length required to reach maximum elastic load carrying capacity), the maximum load carrying capacity is proportional to the maximum shear stress the far end can achieve [6], while for any bond length larger than that but less than the effective bond length, the maximum load-carrying capacity is related to both the peak shear stress and the bond length. Therefore, the temperature-to-loading rate will play a dominant role in determining the maximum load the bonded joints can achieve. As shown in Figs. 17 and 18, the peak shear stress at the far end increases with the loading rate in both scenarios, but they will finally converge to the bond-slip relation at the final temperature. Fig. 19 presents the comparison of the bond-slip behaviour at the loaded end. All cases exhibited the same elastic branch since the peak slip value is reached very fast, compared to the temperature change considered in this study. Significant difference can be observed in the softening range. It should be noted that the bond-slip behaviour was obtained when the full-range behaviour of the bonded joints was calculated for the bond length shorter than the effective bond length [6].

6. Conclusion

This paper has presented a study aimed at investigating the behaviour of CFRP-to-steel bonded joints under mode II loading, and subjected to elevated temperatures. In order to shed light on the effect of different parameters which may affect the behaviour of CFRP-to-steel bonded joints, the effect of bond-slip behaviour, loading-to-heating ratio and the bond length were studied. In order to carry out the analysis, an FDM was proposed and verified using an FEA.

Comparison of the FEM and FDM results verified the suitability of the FDM method in studying the effect of temperature dependent bond-slip relations on the behaviour of CFRP-to-steel bonded joints under elevated temperatures. From the results presented in this paper, the following conclusions can be drawn:

1. The FDM adopted can give satisfactory accuracy in modelling the full-range behaviour of FRP-to-steel bonded joints with varying temperatures;
2. Temperature dependent bond-slip relation was shown to have a significant effect on the behaviour of CFRP-to-steel bonded joints under mode II loading and subjected to elevated temperatures. Using a constant bond-slip behaviour may lead to errors in predicting the accurate bond behaviour;
3. Provided adequate bond length exists, the load carrying capacity of the CFRP-to-steel bonded joints at the final temperature only depends on the interfacial fracture energy at that final temperature;
4. Resultant bond-slip behaviour of the bonded interface varies along the bond length and is dependent on the loading-to-heating rate;
5. Bond length and loading-to-heating rate both may affect the ultimate load as well as the load-displacement behaviour of the CFRP-to-steel bonded joints.

This study was based on assumed bond-slip relation variations with temperature. In order to better understand and design the behaviour of CFRP-to-steel bonded joints, better understanding of the variation of bond-slip parameters, i.e., initial stiffness, peak shear stress, and interfacial fracture energy with temperature is necessary.

In addition, in the bond-slip models used in this study, damaged elasticity was assumed, which has been shown previously to be incorrect. While this assumption may not affect the results under monotonically increasing loads and temperatures, when temperature cycles (increasing and decreasing) and loading cycles (increasing and decreasing) are applied, a temperature dependent bond-slip model accounting for damage and plasticity should be used. Such a model does not exist at this stage, thus more research is required to develop such constitutive models to accurately capture the interfacial bond behaviour under cyclic temperature and loading conditions.

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Notation

The following symbols are used in this paper:

A_p	=	Cross section area of CFRP plate;
A_s	=	Cross section area of steel plate;
B_n	=	Matrix to apply boundary condition at the loaded end;
B_0	=	Matrix to apply boundary condition at the far end;
b_p	=	The CFRP plate width;
b_s	=	The width of steel plate;
C_j	=	The matrix block composed of the first two columns of the coefficients at element j;
D_δ	=	Damage parameter when the slip is δ;
E_p	=	Elastic modulus of CFRP plate;
E_s	=	Elastic modulus of steel plate;
G_f	=	The fracture energy;
h_n	=	Length of the nth segment for the FDM;
K_e	=	Initial elastic stiffness of the bond-slip relation;
L	=	The bond length of CFRP-to-steel bonded joints;
N	=	Axial force of CFRP plate;
Q_j	=	The matrix block composed of the second two columns of the coefficients at element j;

R_j = The external force and thermal expansion at node j

T_g = Glass transition temperature;

t_p = Thickness of CFRP plate;

t_s = The thickness of steel plate;

U_j = The axial force and slip at the node j;

u_p = Displacement of the CFRP plate;

u_s = Displacement of the steel plate;

x = Distance from the loaded end;

Z = The boundary conditions;

α_p = Thermal expansion coefficient of CFRP plate;

α_s = Thermal expansion coefficient of steel plate;

ΔT = Temperature change;

δ = Slip between the CFRP plate the steel plate;

τ = Interfacial shear stress;

τ_f = The maximum interfacial shear stress;

σ_p = Axial stress of CFRP plate;

σ_s = Axial stress of steel plate;

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